RELAP5/3.2 assessment against low pressure onset of flow instability in parallel heated channels

Tewfik Hamidouche a, Anis Bousbia-salah b,*

a Laboratoire des Analyses de Sûreté, Centre de Recherche Nucléaire d’Alger (CRNA), 02 Boulevard Frantz Fanon, B.P. 399, 16000 Alger, Algeria
b Dipartimento di Ingegneria Meccanica, Nucleari e della Produzione, Facoltà di Ingegneria, Università di Pisa, Via Diotisalvi, 2 – 56126, Pisa, Italy

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Abstract

Best Estimate computer codes have been, so far, developed for safety analysis of nuclear power plants and were extensively validated against a large set of separate effects and integral test facilities experimental data relevant to such kind of reactors. Their application to research reactors is not fully straightforward. Modelling problems generally emerge when applying existing models to low pressure and more particularly to subcooled flow boiling situations. The objective of the present work is to investigate the RELAP5/3.2 system code capabilities in predicting phenomena that could be encountered under abnormal research reactor’s operating conditions. For this purpose, the separate effect related to the static onset of flow instability is investigated. The cases considered herein are the flow excursion tests performed at the Oak Ridge National Laboratory thermal hydraulic test loop (THTL) as well as some representative Whittle and Forgan (W&F) experiments. The simulation results are presented and the capabilities of RELAP5/Mod 3.2 in predicting this critical phenomenon are discussed.

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1. Introduction

The nuclear industry has made effective use of advanced modelling and simulation during the design, optimisation, and safety evaluation of nuclear power plants. This has been achievable due to the continuous development and validation of more sophisticated computer codes. However notwithstanding the important progress made in the recent years, the predictions of thermal-hydraulic system codes are not exact but remain uncertain (D’Auria and Galassi, 1998). As a consequence, relevant experimental data simulating phenomenological conditions expected in nuclear plants are needed to assess the validity of the computational models or system codes adopted in the nuclear reactor technology. Generally, such efforts are mostly dedicated for conditions that reflect operation of nuclear power reactors, as the programs sponsored by NEA/OCDE and US NRC and others validation matrixes (Bousbia Salah, 2004). On the other hand, limited published work was devoted for research reactor operating conditions characterized mainly by low pressure operating range, high heat flux, and reduced cooling channel dimensions (Hainoun et al., 1996; Warrier and Dhir, 1999; Kennedy et al., 2000; Hari and Hassan, 2002; Hamidouche et al., 2004). Despite this lack of assessment and validation, few attempts to apply the thermal-hydraulic system code RELAP5 for the safety analysis of research reactors (Woodruff et al., 1996; Bousbia-Salah et al., 2004) have shown, at a first glance, good agreements when compared with some reference channel codes developed specifically for such kind of reactors as PARET (Woodruff, 1984) and RETRAC (Baggoura et al., 1994) codes. However, as a new trend in safety analysis of research reactors, the challenge nowadays is to revisit the safety of research reactors using Best Estimate (BE) computer codes and more particularly coupled 3D neutron kinetic/thermal hydraulic system codes. Focusing on this scope, a Research Coordinated Project (RCP) is being supported by the
International Atomic Energy Agency (IAEA) on safety significance of postulated initiating events on different research reactors and assessment of analytical tools (RCP J7.10.10, 2002). Part of this coordinated project concerns the verification of BE code capability under research reactor operating conditions. In the framework of this RCP, the verification of the RELAP5/Mod 3 series capabilities for the determination of the occurrence of the static flow instability in parallel heated channels has been investigated under low and medium pressure operating conditions. Indeed, most of the research reactors operate at low-pressure levels and are subject to so-called thermal-hydraulic instabilities (THI) or flow excursion (FE) (known as Ledinegg-type instability) (IAEA-TECDOC-233, 1980). The occurrence of the onset of flow instability is due to an increase in the channel pressure drop resulting from high void generation in subcooled boiling regime. In systems subject to FE, initiation of boiling in one channel can lead to flow redistribution in the other adjacent channels and under some critical conditions, this process can induce a flow starvation in the hot channel causing a local dryout or CHF. Consequently, the onset of flow instability (OFI) is considered as a limiting operating parameter for large types of research reactors (IAEA-TECDOC-233, 1980; Rothrock, 1998; Hu and Bernard, 1999). Therefore, accurate determination of conditions that could induce the FE is of great importance for research reactor safety.

According to the established postulate, the OFI corresponds to the minimum of the pressure drop characteristics vs. mass flux curve (Maulbetsch and Griffith, 1965). Also, some authors have established criteria or empirical correlation for the determination of the thermal-hydraulic (T/H) conditions that may induce flow excursion (Whittle and Forgan, 1967; Saha and Zuber, 1974; Duffy and Hugges, 1991). It is also known that the onset of flow instability occurs at a flow rate slightly lower (or a heat flux slightly higher) than the flow rate (or heat flux) at which the onset of significant void occurs. Some authors consider the onset of significant void point as a conservative estimate of the onset of flow instability (Warrier and Dhir, 1999). Some of these criteria are widely used and some of them are implemented in computer codes such as RELAP5/Mod3.

Recently, some authors have focussed their attention to the RELAP5 predictions of low pressure flow boiling (Hari and Hassan, 2002; Yeoh and Tu, 2002; Koncar and Mavko, 2003) and proposed modifications to the code’s subcooled model to account for the effect of the large density ratio at low pressures (about 1600 at one atmosphere pressure) on the model correlations and also on the numerical stability scheme (Warrier and Dhir, 1999).

In the current framework an assessment of the RELAP5 predictions of low pressure flow boiling (Hari and Hassan, 2002; Yeoh and Tu, 2002; Koncar and Mavko, 2003) and proposed modifications to the code’s subcooled model to account for the effect of the large density ratio at low pressures (about 1600 at one atmosphere pressure) on the model correlations and also on the numerical stability scheme (Warrier and Dhir, 1999).

In the current framework an assessment of the RELAP5/Mod 3 subcooled boiling model

The RELAP5/3.2 code was developed for best estimate simulations of transients in water-cooled reactor systems. It is based on a non-homogeneous and non-equilibrium model for two-phase systems that is solved by a fast, partially implicit numerical scheme. The two-fluid formulation, used by the code, includes wall vapor generation. But, since only the bulk fluid conditions are considered, constitutive model adapted to the near-wall region is included to specify the vapor source term in the governing equation. RELAP5 estimates the subcooled void fraction from the interaction of three constitutive models, which are:

- vapour generation (fraction of wall heat flux for vapour generation);
- condensation (in the subcooled fluid);
- interfacial friction.

In the approach adopted in the RELAP5 code, the wall heat flux is partitioned into two components:

\[ Q_w = Q_{\text{convection}} + Q_{\text{boiling}} \]  
(1)

\( Q_{\text{convection}} \) is the fraction of heat flux used for heating the bulk liquid.

The boiling heat flux is defined as:

\[ Q_{\text{boiling}} = Q_{\text{evaporation}} + Q_{\text{pumping}} \]  
(2)

where \( Q_{\text{evaporation}} \) is the portion of the boiling heat flux assigned to void formation; \( Q_{\text{pumping}} \) is the pumping heat flux corresponding to the portion of the boiling heat flux assigned to the bubbles agitation caused by bubbles growth and detachment phenomenon.

The void generation rate \( \Gamma_w \) is derived form the Lahey method as follow:

\[ \Gamma_w = \frac{Q_w(h_{\text{sat}} - h_t)}{V(h_{\text{sat}} - h_t)(h_{\text{sat}} - h_t^e)} \left( \frac{1}{1 + \varepsilon} \right) \]  
(3)

where \( V \) is the node unit volume, \( h_{\text{sat}} \) and \( h_{\text{sat}}^e \) are the saturated liquid and vapor enthalpies, respectively, and \( h_t^e \) is the critical enthalpy depending on the flow regime; and \( \varepsilon \) is the ‘pumping or quenching factor’. The critical enthalpy \( h_t^e \) represents the enthalpy under onset of significant void (OSV) conditions. It is function of Stanton number and is calculated according to the Saha–Zuber correlation (Saha and Zuber, 1974).
The pumping factor is an important parameter in the partitioning of the wall heat flux and is given by:

$$
\epsilon = \frac{Q_{\text{pumping}}}{Q_{\text{evaporation}}} = \frac{\rho_l (h^s_T - \min(h_t, h^s_T))}{\rho_v (h^s_T - h^s_v)}
$$

(4)

where \(\rho_l\) and \(\rho_v\) are the liquid and vapor density, respectively. \(C_p\) is the heat capacity, \(St\) and \(Nu\) are the Stanton and Nusselt numbers, respectively.

As can be deduced from its definition, the pumping factor is very high just above the critical enthalpy and approaches zero as the bulk liquid enthalpy moves toward the saturation value. Pumping causes suppression of the evaporation process and since that, a proper estimation of the pumping factor is necessary to simulate adequately the subcooled boiling phenomenon (Hari and Hassan, 2002).

For the vapor condensation it is governed by the local subcooling as well as the interfacial area concentration and interfacial heat transfer coefficient. In RELAP5/3.2 it is evaluated through the Unal–Lahey mechanistic correlation given by the following formula (RELAP5 Development Team, 1995):

$$
\Gamma_c = \frac{H_d(T^s_T - T_l)}{(h^s_T - h^s_v)}
$$

(5)

where

$$
H_d = \frac{F_3 F_5 (h^s_T - h^s_v) \rho_g \rho_l \gamma}{(\rho_l - \rho_g)}
$$

(6)

The factor \(F_3\) is equal to 1 for the case of liquid subcooling less than 1.0 K. The factor \(F_5\) has been introduced as a smoothing factor between the Lahey and Unal models.

### 3. Experimental set-up and description

In order to assess the RELAP5/3.2 capabilities in predicting the OFI, a set of experimental data performed in the ORNL-THTL and W&F test facilities, are considered. The tests cover different channel dimensions, different pressures (at low levels), different heat flux, and different inlet subcooling.

#### 3.1. ORNL THTL tests

The ORNL – THTL is an experimental facility aimed to support the development of the ORNL advanced neutron source reactor (ANSR).

The primary objective of the THTL was to investigate thermal-hydraulic limits of the ANSR nominal conditions for normal operations and safety margins analysis. The THTL was built and designed to provide a simulated full-length coolant subchannel of the ANSR, allowing experimental determination of the thermal limits (FE and CHF) under anticipated conditions. Furthermore, some tests were performed to assess some conditions related to loss of off-site power and loss of coolant accident. A detailed description of the experimental set up can be found in Felde et al. (1994). A schematic representation of the cross section of the test section is given in Fig. 1. The test section simulates a single subchannel in the ANSR core with a cross section having a full prototype heated length of 50.7 cm, the same flow channel gap of 1.27 mm and the same cladding material (aluminium 6061-T6) with a surface roughness of 0.5 μm. The channel span was scaled down to 12.7 mm to limit total power requirement in the test section during the experiments. A non-uniform axial heat flux distribution is considered during the tests (Siman-Tov et al., 1994).

In the current framework experiments related to flow excursion phenomena performed in the THTL loop are considered. These experiments were conducted under the following T/H conditions:

- coolant: light water, upward flow;
- inlet coolant temperature: 45 °C;
- exit pressure: 1.7 MPa;
- local heat flux range: 0.7–18 MW/m²;
- corresponding exit velocity range: 2.8–28.4 m/s
- channel configuration: rectangular 1.27 × 12.7 × 507 mm.

#### 3.2. Whittle and Forgan tests

In the W&F experiments, demineralised water is circulating through this test loop by mean of a centrifugal circulating pump controlled by a diaphragm valve.

Four of the test sections used by W&F were rectangular with different gap widths as given in Table 1. The test section is connected to the main flow circuit by rectangular flanges brazed at the end of the channel. A cross section of the test section is shown in Fig. 2.

The experiments are first performed at zero power supplied to the test section, the inlet temperature was fixed and the pressure drop across the channel was recorded over a range of flow rates. This procedure was useful in order to assess the measurement instrumentation. The hot power tests are afterwards performed, under uniform axial heat distribution, by setting the flow rate to its maximum value and for a given set of supplied power range to the test...
section. The pressure drop is reported over a range of flow rates including some points in the negative slope of the pressure drop curve. Inlet and outlet temperature were also measured and reported during the experiments.

This procedure was repeated for different test section geometries by varying the gap and the heated length (see Table 1). The inlet temperature was then changed and the above procedure is repeated. The exit pressure was held to 0.117 MPa for most of the tests. The ranges of experimental conditions are:

- coolant: light water, upward flow;
- inlet coolant temperature: variable according to the test;
- exit pressure: 0.117 MPa;
- local Heat flux range: 1.0–2.75 MW/m².

4. Problem modeling

4.1. Description of the nodalization

To perform a numerical simulation of the considered set of experiments, using the RELAP5 code, a nodalization of the test section of the considered loops is performed as shown in Fig. 3. Specified boundary conditions at the inlet and outlet of the test section are considered in order to substitute the remainder components of the test loops (see Table 2). The inlet fluid boundary conditions related to temperature and mass fluxes are fixed using a time dependent volume (TMDPV – 40). This latter is connected to the coolant channel by a branch component (BRANCH – 50). The fixed outlet pressure boundary condition is also imposed by another time dependent volume (TMDPV – 90). This latter is connected to the outlet zone of the coolant channel through a branch component (BRANCH – 80).

In case of THTL, the test section is divided into two heating structures to distinguish between the rectangular part and the rounded part of the heated test section (see Fig. 1). Indeed, in the rounded part of the section the heat flux is only 36% of its value in the rest of the heated surface (Siman-Tov et al., 1994). The cooling channel is represented by a pipe component (PIPE – 100). It is subdivided into 25 equidistant nodes for the THTL test section. The heated channel is connected to the two aforementioned

<table>
<thead>
<tr>
<th>No.</th>
<th>Geometry</th>
<th>Gap width (mm)</th>
<th>Channel width (mm)</th>
<th>LHeated length (mm)</th>
<th>DHydraulic diameter (mm)</th>
<th>Pressure taps distance (mm)</th>
<th>Inlet subcooling (°C)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Rectangular</td>
<td>3.2258</td>
<td>25.4</td>
<td>609.6</td>
<td>5.72</td>
<td>622.3</td>
<td>45</td>
</tr>
<tr>
<td>2</td>
<td>Rectangular</td>
<td>2.4384</td>
<td>25.4</td>
<td>406.4</td>
<td>4.45</td>
<td>482.6</td>
<td>35</td>
</tr>
<tr>
<td>3</td>
<td>Rectangular</td>
<td>2.032</td>
<td>25.4</td>
<td>406.4</td>
<td>3.76</td>
<td>482.6</td>
<td>55</td>
</tr>
</tbody>
</table>
heat structures. The inlet flow variation is specified using a time dependent junction (TMPDJUN – 50) whereas the upper (TMDPV – 90) is used to fix the outlet pressure. The same scheme of nodalization is adopted for the representation of the W&F test loop. However, in this case, the axial node number is only 15 since uniform axial heat flux distribution is considered. According to the authors, only 1% of the total power is applied in the nichrome strips of the heated test section (Forgan and Whittle, 1966).

4.2. Qualification of the nodalization

According to (D’Auria and Galassi, 1998 and IAEA-TECDOC-1387, 2004) before applying a complex thermal-hydraulics code to any T/H system, some pre-conditions must be fulfilled. For instance, a qualified code may predict unrealistic transient scenarios when the nodalization is not properly qualified. The qualification procedure follows a series of iterative processes by means of which the correctness and adequacy of the adopted models are confirmed. To fulfill such conditions, the performed nodalization is assessed against the available experimental data related to the cold test cases where zero power is applied. This will allow also the selection of appropriate input data (i.e., pressure loss coefficients) for the next heated test cases. Simulations results are sketched in Fig. 4 for the THTL test section and in Fig. 5 for W&F test sections. At a first glance, one can notice qualitatively good agreement between the calculated pressure drop and the experimental data related to both experimental setups. However, some minor discrepancies are observed but they did not exceed in the worst case a deviation of 13%. This error is reasonably acceptable since the THTL experimental uncertainties related to the pressure and flow measurements are 5.8% and 5.4%, respectively.

Concerning the W&F cases the agreement is much better, especially in the case of Sections 2 and 3 and also at high Reynolds number. Therefore, the adopted nodalization could be considered suitable for the simulation of the following hot power cases.

5. Results and discussion

All the OFI tests were simulated by the RELAP5/3.2 code. The THTL and W&F pressure drop vs. mass flux curves was reproduced by performing a stepwise flow decrease, beginning from higher values, following a stairway trend. For each step, the flow rate is kept constant dur-
ing 50 s to let the calculations stabilize and again slowly and linearly decreased during 100 s to the next fixed flow rate. The flow is then continuously decreased until the program run is stopped either by trip or by code failure.

5.1. THTL cases

The THTL test cases are characterized by their large inlet subcooling (more than 160 °C) and high heat fluxes, and higher operating pressure in comparison with the W&F cases.

The main simulation results are summarized in Table 3, while Figs. 6 and 7 exhibit the calculated and measured pressure drops across the test sections for different applied powers. In Figs. 6 and 7, only the cases with known experimental data are represented, while for other cases (FE719B, FE318B and FE318B*), only the key parameters are compared in Table 3. At higher heat flux over 10 MW/m², no upturn minimum in the pressure drop vs. mass flux is predicted. This is due to the fact that subcooled dryout is reached leading to excessive test section temperatures in the upper node of the heated channel. This phenomenon was not observed experimentally for heat flux higher than 16 MW/m². The use of different version of RELAP5 such as Mod3.3 did not carry any significant changes in the results. However, according to Siman-Tov et al. (1994) six burnouts cases, such as FE318B*, have been observed during their experiments but they did consider them as preliminary data that need more investigations. As reported in Fig. 6 the calculated pressure drop vs. mass flux curves shows good qualitative representation of the experimental data. However, as could be seen in Fig. 7, for higher heat fluxes (over 2 MW/m²) the agreement became less good.

On the whole, the pressure drop at OFI (the T/H conditions corresponding to OFI are taken at the minima of the channel pressure drop vs. the inlet mass flux curves) is predicted with an absolute error ranging from 5% to 26% for experiments with exit pressure fixed to 1.7 MPa. Whereas, at lower pressure, the deviation is more important (up to 50% for cases FE318B and FE318B*). Concerning the errors related to the estimation of the mass flux at OFI, they range from 1% to 13% for the tests where the operating pressure is 1.7 MPa, and up to 22% for lower pressure cases.

According to the results outlined in Table 3, both the mass flux and the pressure drop at OFI are in general underestimated except for the higher heat flux cases (FE105B and FE719B). Furthermore, the deviation in pressure drop is increasing from higher to lower heat flux i.e., from 5% for the 11.7 MW/m² case to 26% in the case of 2 MW/m².

5.2. W&F cases

The W&F tests differ from the THTL tests by their variable test section dimensions and lower inlet subcooling and operating pressure. Several heat fluxes applied to three different rectangular test sections are considered hereafter. The simulation results are sketched in Figs. 8–10 for test

<table>
<thead>
<tr>
<th>Experiment no.</th>
<th>FE712B</th>
<th>FE714B</th>
<th>FE714C</th>
<th>FE105B</th>
<th>FE719B</th>
<th>FE318B</th>
<th>FE318B*</th>
</tr>
</thead>
<tbody>
<tr>
<td>Parameter</td>
<td>RELAP</td>
<td>EXP</td>
<td>RELAP</td>
<td>EXP</td>
<td>RELAP</td>
<td>EXP</td>
<td>RELAP</td>
</tr>
<tr>
<td>$Q$ (MW/m²)</td>
<td>2.0</td>
<td>5.3</td>
<td>7.4</td>
<td>9.4</td>
<td>11.7</td>
<td>1.9</td>
<td>2.1</td>
</tr>
<tr>
<td>$P_{exit}$ (MPa)</td>
<td>1.7</td>
<td>1.7</td>
<td>1.7</td>
<td>1.7</td>
<td>1.7</td>
<td>0.48</td>
<td>0.44</td>
</tr>
<tr>
<td>$D_p @ OFI$ (kPa)</td>
<td>0.024</td>
<td>0.033</td>
<td>0.126</td>
<td>0.142</td>
<td>0.228</td>
<td>0.28</td>
<td>0.28</td>
</tr>
<tr>
<td>$G @ OFI$ (10³ kg/m²s)</td>
<td>2.47</td>
<td>6.39</td>
<td>7.34</td>
<td>7.42</td>
<td>8.59</td>
<td>10.03</td>
<td>11.21</td>
</tr>
<tr>
<td>$T_{bulk,out}$ @ OFI (°C)</td>
<td>192.37</td>
<td>183.1</td>
<td>192.27</td>
<td>174.32</td>
<td>193.25</td>
<td>182.3</td>
<td>179.25</td>
</tr>
<tr>
<td>$D_{sub,exit}$ @ OFI (°C)</td>
<td>11.9</td>
<td>21.1</td>
<td>12.6</td>
<td>22.1</td>
<td>31.2</td>
<td>31.6</td>
<td>31.8</td>
</tr>
<tr>
<td>Void @ OFI (%)</td>
<td>7.06</td>
<td>20</td>
<td>26.1</td>
<td>31.5</td>
<td>32.4</td>
<td>33.5</td>
<td>33.8</td>
</tr>
<tr>
<td>$T_{Wall max}$ (°C)</td>
<td>225.06</td>
<td>214.39</td>
<td>243.31</td>
<td>225.59</td>
<td>252.43</td>
<td>228.37</td>
<td>250.45</td>
</tr>
<tr>
<td>$V_{at exit}$ (m/s)</td>
<td>3.04</td>
<td>2.9</td>
<td>3.04</td>
<td>2.9</td>
<td>3.04</td>
<td>2.9</td>
<td>3.04</td>
</tr>
<tr>
<td>$St_{at exit}$</td>
<td>0.016</td>
<td>0.012</td>
<td>0.016</td>
<td>0.012</td>
<td>0.016</td>
<td>0.012</td>
<td>0.016</td>
</tr>
</tbody>
</table>

Table 3: Main calculated parameter for THTL

- Void fraction calculated with modified ATHLET code (Hamoun and Schaffrath, 2001).
- Last calculated values before failure.
- Actual FE burnout.
sections 1–3 (TS1, TS2, and TS3), respectively, and the main calculated parameters are summarized in Table 4. In this case, the agreement for the pre-OFI phase with measurement is good for TS2 and TS3. For TS1 case the agreement is less good as already observed for the cold case. The deviations at the OFI point range between 2 and 21 KPa for the channel pressure drop. These errors are acceptable since the pressure drop measurement errors are around 4 KPa. For the mass flux, the maximal deviation is about 42% for TS1, 30% for TS2 and 38% in case of TS3 tests. These deviations seem to be function of the applied heat flux and the inlet subcooling; the higher is the subcooling the larger is the simulation discrepancy. This fact confirms the errors tendency observed in the THTL cases. In fact, the deviations trend obtained for the TS3 cases (high inlet subcooling) is the same as that observed in the THTL sim-

Fig. 6. Channel pressure drop for an applied heat flux of 2 MW/m².

Fig. 7. Channel pressure drop for THTL hot cases.

Fig. 8. Pressure drop characteristic curve for W&F TS1.

Fig. 9. Pressure drop characteristic curve for W&F TS2.

Fig. 10. Pressure drop characteristic curve for W&F TS3.
ulation cases; the mass flux and pressure drop at OFI are almost always under predicted \( G_{\text{cal}}^{\text{OFI}} < G_{\text{exp}}^{\text{OFI}} \).

On the other hand, among the findings coming from W&F test data and investigation, a correlation threshold for OFI occurrence was presented by (Whittle and Forgan, 1967). They stated that the threshold conditions for flow instability are determined in term of a critical value of the ratio \( R \) defined as:

\[
R = \frac{1}{1 + \eta^{\text{HI}}} 
\]

where \( \eta \) is the Bowring’s criterion for the change in regime (also known as the bubble detachment parameter) and \( D_H \) the heated equivalent diameter and \( L_H \) the heated length.

This correlation is being widely used for research reactor OFI determination (Hu and Bernard, 1999; IAEA-TEC-DOC-233, 1980). The critical value for OFI corresponds to \( \eta = 25 \); this value was obtained by Whittle and Forgan by fitting their data and data of Maulbetsch and Griffith (1965) and it was suggested as a limiting value for OFI threshold.

From W&F experimental data, \( \eta \) lies between 20 and 27 as mean values for rectangular test sections with the ratio \( L_H/D_H \) between 100 and 200.

According to (Whittle and Forgan, 1967), the ratio \( R \) is also defined as:

\[
R = \frac{\Delta T_c}{\Delta T_{\text{sat}}} = \frac{T_{\text{out}} - T_{\text{sat}}}{T_{\text{sat}} - T_{\text{inlet}}} 
\]

where \( T_{\text{inlet}} \), \( T_{\text{out}} \) and \( T_{\text{sat}} \) are the inlet, outlet and saturation temperature, respectively.

Using this later definition, the values of \( \eta \) as calculated from RELAP5 outputs are reported in Table 4. As could be noticed the values of \( \eta \) lies between 20 and 27, values that are very close to the W&F recommended limit. Thus, even though the calculated data are generally overestimating the minimum mass flux, the RELAP5 output give acceptable estimation of the critical ratio \( \eta \) if used as a limiting condition for OFI in the range of T/H conditions considered herein.

5.3. Interpretation of the simulation results

On the whole, one can notice that, independently from the operating pressure and the test section geometry, the error trend is function of the inlet subcooling and of the applied heat flux; the higher are the subcooling and the heat flux, the larger is the deviation between the predictions and the measurements.

Reason for such trends could have several origins. In fact, under low pressure conditions, a small error can lead to large variations in the void fraction or large oscillations in the numerically calculated void fraction (Warrier and Dhir, 1999). Therefore, it is difficult to locate accurately the source of the observed discrepancies. This latter could be, in our opinion, related to one or to the combined effect of:

- The code model related to the interfacial drag terms. This correlation contains several empirical coefficients, and most of them were derived under high-pressure flow boiling situations.
- The wall friction factor, which is estimated using the Colebrook correlation (RELAP5 Development Team, 1995) that do not take into account the effect of the fluid boundary layer properties which differ from the bulk liquid ones.

<table>
<thead>
<tr>
<th>Test section type</th>
<th>Parameter</th>
<th>RELAP</th>
<th>EXP</th>
<th>RELAP</th>
<th>EXP</th>
<th>RELAP</th>
<th>EXP</th>
<th>RELAP</th>
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<th>RELAP</th>
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<th>RELAP</th>
<th>EXP</th>
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<tbody>
<tr>
<td>TS1</td>
<td>( q'' ) (MW/m²)</td>
<td>2.50</td>
<td>1.80</td>
<td>1.40</td>
<td>1.04</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>DP @ OFI (kPa)</td>
<td>53.77</td>
<td>35.72</td>
<td>34.07</td>
<td>20.73</td>
<td>22.98</td>
<td>11.75</td>
<td>16.07</td>
<td>6.0</td>
<td></td>
<td></td>
<td></td>
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</tr>
<tr>
<td></td>
<td>G @ OFI (10³ kg/m³s)</td>
<td>5853</td>
<td>5400</td>
<td>4440</td>
<td>4011</td>
<td>3658</td>
<td>3076</td>
<td>2500</td>
<td>1750</td>
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<td></td>
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</tr>
<tr>
<td></td>
<td>( T_{\text{bulk,out}} ) (°C)</td>
<td>93.7</td>
<td>96.1</td>
<td>92.5</td>
<td>95</td>
<td>90.8</td>
<td>96.5</td>
<td>92.6</td>
<td>94.6</td>
<td></td>
<td></td>
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</tr>
<tr>
<td></td>
<td>( \eta )</td>
<td>24.9</td>
<td>25.2</td>
<td>20.7</td>
<td>20.8</td>
<td>23.5</td>
<td>25.2</td>
<td>21.5</td>
<td>22.1</td>
<td></td>
<td></td>
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<tr>
<td>TS2</td>
<td>( q'' ) (MW/m²)</td>
<td>2.10</td>
<td>1.80</td>
<td></td>
<td>1.60</td>
<td></td>
<td></td>
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<tr>
<td></td>
<td>DP @ OFI (kPa)</td>
<td>47.89</td>
<td>28.33</td>
<td>35.45</td>
<td>21.59</td>
<td>30.69</td>
<td>9.73</td>
<td></td>
<td></td>
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<tr>
<td></td>
<td>G @ OFI (kg/m³s)</td>
<td>5800</td>
<td>4730</td>
<td>4864</td>
<td>4013</td>
<td>3567</td>
<td>2755</td>
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<td></td>
</tr>
<tr>
<td></td>
<td>( T_{\text{bulk,out}} ) (°C)</td>
<td>95.0</td>
<td>NA</td>
<td>94.0</td>
<td>NA</td>
<td>92.5</td>
<td>NA</td>
<td></td>
<td></td>
<td></td>
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<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>( \eta )</td>
<td>22.0</td>
<td>22.1</td>
<td>23.2</td>
<td>23.4</td>
<td>23.3</td>
<td>25.2</td>
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<tr>
<td>TS3</td>
<td>( q'' ) (MW/m²)</td>
<td>2.75</td>
<td>2.18</td>
<td>1.77</td>
<td>0.66</td>
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</tr>
<tr>
<td></td>
<td>DP @ OFI (kPa)</td>
<td>50.91</td>
<td>60.85</td>
<td>35.49</td>
<td>40.67</td>
<td>25.89</td>
<td>27.30</td>
<td>6.68</td>
<td>5.2</td>
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</tr>
<tr>
<td></td>
<td>G @ OFI (kg/m³s)</td>
<td>5697</td>
<td>4430</td>
<td>4651</td>
<td>3536</td>
<td>3856</td>
<td>2792</td>
<td>1220</td>
<td>1170</td>
<td></td>
<td></td>
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<td></td>
</tr>
<tr>
<td></td>
<td>( T_{\text{bulk,out}} ) (°C)</td>
<td>91.3</td>
<td>NA</td>
<td>90.0</td>
<td>NA</td>
<td>89.1</td>
<td>NA</td>
<td>96.8</td>
<td>NA</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td></td>
<td>( \eta )</td>
<td>26.7</td>
<td>26.6</td>
<td>24.2</td>
<td>26.6</td>
<td>23.5</td>
<td>26.6</td>
<td>26.7</td>
<td>28.2</td>
<td></td>
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</tr>
</tbody>
</table>
• The wall partitioning subcooled model of the code since the calculated bulk liquid temperature at the outlet is overestimated by more than 10 °C (in the THTL cases). This large difference may reflect a non-adequate model for partitioning the heat used for void generation and the heat used for heating the bulk liquid. Indeed, the reliability of the wall vapor generation model is questionable. The correct approach to partitioning the wall heat flux is still an open question (Warrier and Dhir, 1999).

• The subcooled condensation model. In fact the amount of void present in the channel governs the OFI phenomenon. Therefore, an adequate model for the void condensation rate should give better fit of the experimental data. Since no measured void fractions are provided, comparisons are carried out by considering the ATHLET code results made for the same cases. This latter was specially modified by (Hainoun and Schaffrath, 2001) to better fit the experimental data by implementing a more suited subcooled condensation boiling model for research reactors operating conditions.

In Table 3, results for void fraction as predicted by RELAP5 code and ATHLET are outlined. As it could be noticed, discrepancies exist between the results of the two codes. These differences could constitute an origin of the observed RELAP5 deviations with respect to experimental measurement since better results are obtained by the ATHLET code.

• On the other hand, the observed differences could also be also due to the adopted definition for the OFI.

As reported in Fig. 11, the calculated Stanton number (at the channel exit) exceeds the Saha and Zuber criteria for OSV that correspond to \( St = 0.0065 \). But, if we consider, as suggested by many authors (Kennedy et al., 2000), that the OFI appears at OSV, then the corresponding data, as outlined in Table 5, agree better with the experimental ones.

6. Sensitivity analyses

In order to check the code prediction capabilities related to the FE experiments, a series of sensitivity analyses are performed. In the current study it was not possible to investigate the embedded code subcooled boiling models, which may have a big impact on the code predictions (Warrier and Dhir, 1999; Kennedy et al., 2000; Hainoun and Schaffrath, 2001). Therefore, the sensitivity evaluations are performed based upon uncertain thermal-hydraulic parameters related to the test facilities, as well as to some key initial and boundary operating conditions. The reference calculation case is chosen to be the W&F high heat flux TS3 case since it exhibits the largest deviation with respect to measurements. The sensitivity analyses are carried out by altering the following parameters chosen due to their prevalent impact on the occurrence of the OFI phenomenon.

- **Case 1.** The inlet coolant subcooling. The effect of such parameter is significant as discussed before. In fact, both the void generation and condensation rate are function of the inlet subcooling. The effect of this parameter is assessed by varying the inlet temperature by ±2 °C in order to take into account eventual experimental uncertainties.

- **Case 2.** The total supplied power. The power is varied by ±5%. This variation was chosen to include the effect of heat losses as well as uncertainties relative to the heated length and/or the perimeter of the heated test section.

- **Case 3.** The pressure at the upper plenum. The effect of the operating pressure on the local saturation temperature and therefore the subcooling is significant. Such effect is assessed by increasing the outlet pressure by ±10%.

<table>
<thead>
<tr>
<th>Table 5</th>
<th>Main calculated parameter for THTL according to S&amp;Z criteria (( St = 0.0065 ))</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Parameter</strong></td>
<td><strong>Experiment no.</strong></td>
</tr>
<tr>
<td></td>
<td><strong>FE712B</strong></td>
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<tr>
<td>( Q ) (MW/m²)</td>
<td>RELAP</td>
</tr>
<tr>
<td>DP @ OFI (kPa)</td>
<td>0.024</td>
</tr>
<tr>
<td>G @ OFI (10³ kg/m²s)</td>
<td>2.64</td>
</tr>
<tr>
<td>( T_{bulk,out} ) @ OFI (°C)</td>
<td>183.71</td>
</tr>
<tr>
<td>( \Delta T_{sub,ext} ) @ OFI (°C)</td>
<td>20.69</td>
</tr>
<tr>
<td>( T_{wall,max} ) (°C)</td>
<td>223.46</td>
</tr>
</tbody>
</table>

Fig. 11. Position of OFI according to Saha and Zuber criteria.
7. Conclusion

The revisiting of the safety margins of research nuclear reactor is currently triggered by the extensive and large utilisation of BE computers codes and more particularly coupled 3D neutron kinetic/thermal-hydraulic system codes. The current study constitutes a contribution of extending the use of BE tools and concerns particularly the verification of the RELAP5/Mod 3 series capabilities for predicting critical phenomena under thermal hydraulic situations relevant to research reactors operating conditions.

For this purpose the static onset of flow instability is considered. In the THTL recalculation cases, the RELAP5 results show acceptable discrepancies when lower heat fluxes are applied to the test section. The comparison with respect to the W&F experimental data shows the same error magnitude and trends. However, the correlation established by W&F is confirmed by the results obtained by RELAP5 even if the threshold for void formation was the Saha–Zuber criteria.

The sensitivity analysis showed the influence of uncertainties related to the inlet subcooling and the hydraulic diameters on the OFI prediction. However, the wall heat flux fraction model for the generation of the subcooled void could also be a source of the code prediction discrepancies since the calculated outlet bulk liquid temperature, when the inlet subcooling is high, is generally overestimated by almost 10 °C.

Therefore, the current study indicates that the use of RELAP5 in predicting the OFI under research reactors operating conditions is not straightforward and needs to be complemented by additional experimental validation and model enhancement and verification.

Acknowledgements

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